Primary Creep Crack Growth at Room Temperature in Surface Cracked Pipes

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1 INTRODUCTION

Proof tests in ductile materials may cause a crack to grow without failing the component. The crack may grow to a length which will lead to component failure during service at relatively moderate loads due to the history-dependent damage which occurs at the crack tip during the proof test. Time-dependent crack growth greatly enhances this effect. Hence, it is very important to be able to quantify the effects of time dependent, as well as time-independent crack growth and damage which occurs during a proof test. The effect of this damage on the subsequent service loading also must be understood.

2 A PRIMARY CREEP CRACK GROWTH MODEL

Details of the primary creep crack growth model were described previously (Brust and Leis, April 1990; Brust and Leis, 1991; and Brust and Leis, June, 1990). Here a short overview is provided, but the preceding references should be consulted for detailed verification.

In a body which is loaded as in Fig. 1, a plastic zone develops during the

FIGURE 1. ILLUSTRATION OF DEFORMATION BEHAVIOR NEAR A CRACK TIP DURING A LOAD-HOLD SEQUENCE

initial loading. For instantaneous power law plasticity, the total strain is represented in the usual way. Since plastic strain dominates the stress-strain response near the crack tip, the near-tip stresses thus resemble the well-known HRR field:

$$\sigma_{ij} = J^{\left(\frac{1}{n_p+1}\right)} r^{-\left(\frac{1}{n_p+1}\right)} f_{ij}(\theta, n_p) \quad (1)$$

as long as 'r' is small.

For $t > 0$, creep straining begins as the load is held constant. The simplified power law constitutive response in rate form after $t > 0$ is represented as:

$$\dot{\epsilon}_{ij} = \dot{\epsilon}_{ij}^e + \dot{\epsilon}_{ij}^p + \dot{\epsilon}_{ij}^c \quad (2a)$$

$$\dot{\epsilon}_{ij}^e = \frac{1+\nu}{E} \dot{\sigma}_{ij} - \frac{\nu}{E} \dot{\sigma}_{kk} \delta_{ij} \quad (2b)$$

$$\dot{\epsilon}_{ij}^p = \frac{3}{2} B_0 n_p \sigma_e^{(n_p-2)} \dot{\sigma}_e S_{ij} \quad (2c)$$

$$\dot{\epsilon}_{ij}^c = \frac{3}{2} B_1 \sigma_e^{(n_c-1)} (\epsilon_e^c)^{-p} S_{ij} \quad (2d)$$

In eqs. (2), a superimposed '.' represents the material time derivative, E and ν represent the elastic modulus and Poisson's ratio, B_0 and n_p represent power-law plasticity material constants, S_{ij} is the stress deviator, σ_e is equivalent stress, and N_c , p , and B_1 represent material constants for a strain hardening creep law. (Time hardening has also been considered, as detailed in Brust and Leis, April, 1990; Brust and Leis, 1991; and Brust and Leis, June, 1990.)

For $t > 0$, creep straining begins. If $n_c > n_p$ (which is the situation examined extensively in the literature), the third term of eq. (2a) dominates very near the crack tip, and a creep zone develops at the crack tip which grows with time. The crack tip stress state thus relaxes, and the crack tip stress state becomes another HRR field growing within the plastic HRR field (see, for instance, Reidel, 1981).

Consider the case of $n_c < n_p$, which is a requirement for the present analysis procedure. For this case a plastic zone may develop at the crack tip upon loading at time $t = 0$. After the hold period begins, the stress level very near the crack tip increases rather than decreases, and the effective plastic zone increases in size. That this is so may be seen by observing that the second term of eq. (2a) dominates the first and the third terms. Hence, the plastic HRR field of eq. (1) dominates the near-field stress and strain states. Hence, rather than going from a 'plastic' HRR field to a 'creep' HRR field which occurs for $n_c > n_p$ and results in stress relaxation (here for $n_p > n_c$), the stress state remains the 'plastic' HRR field for $t > 0$, and the stress state increases since the strength of the HRR field, J, increases. Thus 'loading' or 'stressing' takes place in the vicinity of the crack tip despite the fact that the far-field load remains constant in time*. The above-described processes were illustrated in detail (Brust and Leis, April, 1990, and Brust and Leis, 1991) where detailed finite element analyses of cracked bodies were performed.

Since loading or stressing takes place during the hold periods, the J-integral should still govern the strength of the HRR field and, hence, govern the crack initiation and growth process for this situation. Crack initiation, growth, and instability are characterized within the J tearing theory and related limitations (Hutchinson, 1983; and Paris and Hutchinson, 1979), and the realization that reasonable predictions are obtained even if the conditions (Paris and Hutchinson, 1979) are violated (see Brust, 1987 and the numerous references cited therein).

3 SIMPLE MODEL - CRACK DRIVING FORCE FOR TWO-DIMENSIONAL PROBLEMS

Consider cases where $n_p > n_0$, and loading ensues at the crack tip during the hold periods; i.e., the stresses at the crack tip increase as creep occurs. Since the loading which occurs does not constitute a drastic change in loading path (proportionally) at each material point, we may assume that the material does not exhibit memory under these circumstances. Hence, we assume that the stress state that is achieved after loading and subsequent hold at any given instant of time t_1 is not greatly different from that achieved by assuming that, at time t_1 , the specimen was loaded assuming time-independent response but with material properties representative of those for the current time.

A Ramberg-Osgood stress strain relationship for elastic-plastic time-dependent response may be written in the following form:

$$\epsilon/\epsilon_0 = \frac{\sigma}{\sigma_0} + \alpha(t) \left(\frac{\sigma}{\sigma_0} \right)^{n(t)} \quad (3)$$

where $\epsilon_0 = \sigma_0/E$, σ_0 is an arbitrary reference stress, and $\alpha(t)$ and $n(t)$ are material parameters that are functions of time. We estimate the time-dependent value of J by using the engineering procedure (reference [18]) as:

$$J = J_e + J_p(t)$$

$$J_p(t) = \alpha(t) \sigma_0 \epsilon_0 c h_1 \left(\frac{P}{P_0} \right)^{n(t) + 1} \quad (4)$$

In eq. (4), c is the uncracked ligament, P is load and P_0 is a reference load (Kumar, et al., 1981). The time-dependent values for α and n are obtained by developing Ramberg-Osgood constants for isochronous stress-strain curves at discrete instances of time (see Brust and Leis, 1991).

The two-dimensional analysis procedure may be summarized as follows. When the specimen is initially loaded, J is estimated with n and α defined for $t = 0$. Thereafter, $\alpha(t)$ and $n(t)$ are used in eq. (3) and eq. (4) then used to estimate J for time = t . By time marching, one may estimate J as a function of time until the end of the hold period. If at the end of the hold period more load is applied, the procedure starts over again. Theoretical justification and further details are elaborated upon (Brust and Leis, April, 1990; Brust and Leis, 1991; and Brust and Leis, June, 1990).

In summary, J is calculated by time marching. Crack growth ensues when $J = J_{IC}$, and crack instability occurs when $dJ/da > dJ_R/da$. Of course, if global unloading or large amounts of crack growth occur, this assumption is violated and the associated results may be in error. The implications of these assumptions are explained in detail in the references cited previously.

*Load (or stress) shedding occurs away from the crack.

4 THE PRIMARY MODEL FOR AXIAL PART-THROUGH CRACKED PIPES

Because estimation schemes are not available for evaluating J for surface cracks, the primary creep model is based upon a modification of elastic solutions for J . For elliptic surface cracks subjected to tensile loading, detailed compilations of finite element solutions are readily available in the literature if elastic conditions prevail. The primary creep model consists of estimating J by using a time-dependent Irwin estimate of the plastic zone inserted into the elastic solution. Hence the method relies on the small-scale yielding assumption. However, reasonable and conservative predictions result when the method is applied outside this region of strict applicability.

The elastic component is written as:

$$J_e = \sigma_H^2 \left(\frac{\pi a}{Q} \right) \cdot \{F(a/c, a/t, \phi)\}^2 \left(\frac{1}{E(1-\nu^2)} \right) \quad (5)$$

where a is crack depth, $2c$ is crack length, t the pipe wall thickness, σ_H is the applied hoop stress caused by internal pressure loading, and ϕ defines the location along the crack to evaluate J_e (see Figure 2 for geometry definition).

FIGURE 2. GEOMETRIC PARAMETERS FOR PART-THROUGH-WALL FLAWS

In equation (5), Q is the square of the complete elliptic integral of the second kind as approximated (Newman and Raju, 1981). F in equation (5) was developed (Stonesifer, et al., 1991) using the finite element alternating method for a pipe R/t ratio of 40, which is typical for gas line pipe of concern here.

The time-dependent nonlinear component of J for external axial- surface cracked pipes is estimated by placing a plastic zone correction on the elastic solution.

The size of the plastic zone ahead of the crack tip has been estimated by analogy to the approach suggested by Kujawski and Ellyin (Kujawski and Ellyin, 1986), as detailed (Brust and Leis, April, 1990; Brust and Leis, 1991; and Brust and Leis, June, 1990).

Within that framework, the nonlinear component of J , $J_p(t)$ is given by:

$$J_p(t) = (\sigma_H)^2 \left(\frac{\pi(r_p(t))}{QE/(1-\nu^2)} \right) \{F(a/c, a/t, \phi)\}^2 \quad (6)$$

where $r_p(t) = r_p(n(t), \alpha(t))$, the other parameters are defined earlier and the function F is given (see Stonesifer, et al, 1991). The value of J is then found as the sum of eqs. (5) and (6).

Finite-element analyses were performed to examine the appropriateness of the analysis assumptions which are inherent in the primary creep damage model for surface cracks. As detailed (see Brust and Leis, April, 1990; Brust and Leis, 1991; and Brust and Leis, June, 1990), these results indicate the assumptions and

simplifications embedded in the time marching estimation scheme provide reliable engineering estimates of crack driving force. Further assessment of the utility of this simplified scheme follows in comparisons of predictions for a full-scale test, details for which can be found (see Brust and Leis, in review; and Leis, Brust, and Scott, 1990).

5 TEST RESULTS AND MODEL PREDICTIONS AND EVALUATIONS

Figure 3 presents a typical comparison of predicted and observed flaw initiation, growth, and failure behavior on coordinates of pressure and flaw extension. Note that the predictions are conservative, with crack initiation predicted earlier than the corresponding experimental results. The predicted final crack length is conservative slightly as is the failure pressure. However, both compare reasonably well with the experimental results. Figure 3 shows that much of the crack growth occurs during the hold periods, which means that neglecting time-dependent effects could lead to nonconservative predictions of crack growth and failure. Also shown here is the failure prediction using the empirical equation developed by Kiefner (Kiefner, et al., 1973), where $CVP = 32$, and the flow stress is 73,700 psi (508 MPa). The method (see Kiefner, et al., 1973) which does not include crack growth effects explicitly and ignores time-dependent effects, also underpredicts the failure pressure. Comparisons of observed and predicted behavior covering a range of flaw geometries have been made (Brust and Leis, April, 1990; Brust and Leis, in review; and Brust and Leis, June, 1990). These results show that the model generally predicts crack initiation at pressures slightly lower than the experimental initiation pressure.

FIGURE 3. OBSERVED AND PREDICTED FLAW GROWTH FOR AXIAL PART THROUGH-WALL FLAWS

6 CONCLUSION

Here the importance of primary creep crack growth is illustrated via experimental data and through a simple engineering model. The simple engineering model provides reasonable predictions of experimental data despite the major assumptions upon which it is based. (Leis, et al., 1990) provides more than fifty predictions of axial surface crack growth and failure in pipe and corresponding comparisons to experimental data developed from full-scale tests. These full-scale tests were made over the last twenty years. It was found that the model presented here provides good predictions of failure load for almost every test.

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